Abstract
A test specimen for measurement of the critical energy release rate associated with longitudinal compressive failure is proposed. High strains are localized by decreasing the out-of-plane thickness towards the anticipated damage region which consists of a unidirectional (UD) laminate. Thus, the compressive fibre failure mode is isolated. Microscopic studies show that the UD-material fails in a kinking mode. A method based on a generalized form of the J-integral and full-field measurements of the strain field is developed to extract the fracture energy. The fracture energy in four experiments is measured to be 20-40 kN/m. Finite element simulations are performed to validate the experimental results. The essential features of the response are captured by modelling the damage region with cohesive elements.

1 Introduction
A major concern in structural design of Carbon Fibre Reinforced Polymers (CFRP) components is longitudinal compressive failure. CFRP are in general a brittle material and instead of plastic deformation, stresses around e.g. holes are relaxed by progressive damage in a fracture process zone. The redistribution of stresses allows further loading after damage initiation. Thus, ignoring this process with an elastic stress analysis underestimates the load capacity.

To model the structural integrity, the fracture energy needs to be known. However, no test method has yet been standardized for determination of the fracture energy associated with fibre dominated compressive failure. From centre-cracked compression tests with a T800/924C laminate and (0,90)_3s layup reported in [1], Pinho et al. derive the fracture energy for kink-band formation to about 76 kJ/m^2 [2]. In [2], Pinho et al. use a compact compression (CC) specimen with a T300/913 laminate with cross ply layup and the fracture energy 79.9 kJ/m^2 associated with the 0°-plies is reported. In the presented method, a normalized energy release rate is calculated from linear FE-simulations. The fracture energy is then directly determined from the maximum load. Later in [3], Catalanotti et al. use the same specimen and layup geometry with a different material system (Hexcel IM7-8552) and the fracture energy 47.5 kJ/m^2 associated with the 0°-plies is reported. The authors use digital image correlation to calculate the fracture energy from the actual strain field on the lateral surface of the specimen, i.e. the assumption of a small scaled damage zone is avoided. However, the use of cross ply laminates necessitates partitioning of the fracture energy since the energy dissipated in the 90°-plies needs to be deducted. Furthermore, interaction effects between the alternating 0°-plies and 90°-plies are neglected. Therefore it is desired to determine the fracture energy by the use of a UD-layup. A test method using a four point bend specimen with a UD-layup is presented by Laffan et al. in [4]. The material system is the same as in [3]. Also here, linear elastic FE-simulations are used to determine how the energy release rate relates to the applied load. The experiments are interrupted when damage initiation is first detected. A fracture energy of 25.7 kJ/m^2 is reported for initiation of compressive failure. Compressive failure is initiated by a shear crack at the notch which propagates a small distance with the direction of about 45° to the mode I direction and later transforms into a kink band as the load increases. This transition phase is described in [5] by Gutkin et al.
In compression tests, the strain is usually localized by the use of a premade notch that achieves a stress rising effect. In the present work, the stress field is not localized from the tip of a premade notch. Instead, the out-of-plane thickness is decreased towards the anticipated damage region and longitudinal compressive failure is obtained in a region with a UD-layup. The data reduction scheme is derived by using a generalized form of the J-integral [6]. Thus, no assumption of a small damage zone is made.

FE-simulations are performed to validate the experimental results where a cohesive zone models the damage region. Governing parameters of the cohesive law are estimated from experimental results and the simulations capture the main features of the experimental behaviour.

2 Design of specimen and test setup

The geometry of the specimen is shown in Fig. 1. The specimens are manufactured using Resin Transfer Moulding (RTM) with a UD fabric reinforcement made from HTS carbon fibres and RTM6 resin.

Longitudinal compressive failure occurs in a region at the mid-section of the specimen consisting of a UD-layup. This region with a height of 1 mm and thickness of 0.8 mm, cf. Fig. 1, is the following denoted the waist. The nominal layup, indicated by L1, is (0/±60/0/±60/0) with the 0°-direction parallel to the loading direction. To achieve the UD-layup in the waist, layup L2 is first created by dropping off the four inner ±60°-plies. Closer to the mid-section, the outer plies including the remaining ±60-plies are removed with a milling operation.

In Fig. 2, the test-setup is shown. Compressive loading is applied to the specimen by using a LLOYD loading frame with a 10 kN load cell. The free arms of the specimen are inserted between two steel plates to make sure that the load is symmetrically applied and also to prevent out-of-plane deformation. The load is transferred to the specimen by inserting solid cylinders with a slightly smaller diameter into the holes in the plates and the specimen. A prescribed displacement of 50 µm/min is applied on the upper cylinder while the lower cylinder is held fixed.

A digital image correlation system (Aramis) is used to monitor the displacement field on the lateral surface of the specimen. The monitored region is sufficiently large so that displacements of the loading points are measured simultaneously with the displacement field of the highly strained region between the free arms of the specimen. The displacement field is recorded every five seconds and about 300 pictures are taken during an experiment.

3 Data reduction

A generalized form of the J-integral [6] is introduced in terms of the surface integral,

\[ P = \iint_{\Omega} (W n_1 - T_i u_{i,1}) \, d\Omega \]  

(1)

where \( W \) is the strain energy density, \( T_i \) is the traction vector, \( u_i \) is the displacement vector and \( n_1 \) is the \( x \)-component of the outward normal of the surface \( \Omega \). Indices 1, 2 and 3 indicates components in the \( x \)-, \( y \)- and \( z \)-direction, respectively. Summation is indicated by repeated indices and partial differentiation by a comma. It can be shown that the P-integral is zero when evaluated over a closed surface if the enclosed volume does not include any singularity in terms of an object or feature that changes the potential energy of the body when moved in the elastic field. The proof is similar to the procedure for the 2D-case and the J-integral [6]. The P-integral can also be interpreted as the sum of all configurational forces that act on the volume with the outer boundary \( \Omega \).

In the following, the lateral surfaces of the test specimen are partial surfaces of \( \Omega \). These surfaces do not contribute to \( P \), since \( n_1 = 0 \) and \( T = 0 \). Thus, Eq. (1) is simplified to

\[ P = \oint_S (W n_1 - T_i u_{i,1}) \, dsdz \]  

(2)

where \( S \) is the surface created when an integration path in the \( xy \)-plane cuts through the thickness, and \( ds \) is an element of arc length along the in-plane integration path. That is, the P-integral and the J-integral are analogous; with the only difference that the P-integral necessitates integration over the, in the present case, varying out-of-plane thickness. That is, if the P-integral is evaluated along a counter-
clockwise integration path connecting the crack surfaces, it represents the energy release rate in terms of the potential energy released for a unit propagation of the crack.

The present test geometry does not contain a crack. However, a crack-like damage zone is anticipated along the waist and a crack-like propagation takes place along the waist when compressive failure occurs. Thus, it is assumed that the energy release rate can be determined from evaluation of $P$ with Eq. (2) along a counter-clockwise integration path connecting points L and U in Fig. 3. To be specific, points L and U are symmetrically positioned with a distance $h_{\text{waist}}$, i.e. at the lower and upper points of the waist that coincides with the outer boundary of the specimen.

Equation (2) is evaluated along two integration paths that are indicated by $\Gamma_1$ and $\Gamma_2$ in Fig. 3. In the evaluation along integration path $\Gamma_2$, the path is shrunk to the vertical line from point L to point U. Thus, $P$ only depends on the local field within the waist and the corresponding configurational force is in the following denoted $P_{\text{waist}}$. The outer surface along $\Gamma_2$, $S_{\text{waist}}$, is not subjected to tractions, i.e. the evaluation of Eq. (2) along $\Gamma_2$ yields

$$P_{\Gamma_2} = P_{\text{waist}} = \iint_{S_{\text{waist}}} W \, dy \, dz$$

Since the out-of-plane thickness of $S_{\text{waist}}$ is constant, the nominal energy release rate is given by

$$\bar{J}_{\text{waist}} = P_{\text{waist}} / B = \int_{\Gamma_2} \bar{W} \, dy$$

where $B$ is the out-of-plane thickness of the waist and $\bar{W}$ is the average of the strain energy density across the out-of-plane thickness.

Now, Eq. (2) is evaluated along the integration path $\Gamma_1$, cf. Fig. 3. The path $\Gamma_1$, coincides with the outer boundary of the specimen and the boundaries along the holes. The surfaces that include boundaries B-A, D-C, F-E and H-G, do not give any contribution to Eq. (2) since $n_1 = 0$ and $T = 0$ on these surfaces. Moreover, the specimen is designed so that the surfaces that include the vertical boundaries A-H, E-D and C-B are virtually unstrained, i.e. $W = 0$ and $T = 0$. That is, these surfaces do not contribute to Eq. (2). Thus, along $\Gamma_1$, the only contributions to Eq. (2) are associated with the surfaces along the holes and the surfaces that include boundaries b and r that is indicated to the right in Fig. 3. Boundaries r coincides with the curved outer boundary of the specimen and boundaries b consists of the vertical boundaries that connects the waist with boundaries r. Summing all configurational forces that acts along $\Gamma_1$ results in

$$P_{\Gamma_1} = P_{\text{load}} + P_b + P_r$$

The path independence of Eq. (2) implies that the configurational forces obtained from Eq. (3) and Eq. (5) are equal. By using Eq. (4), the nominal energy release rate associated with the waist is given by

$$\bar{J}_{\text{waist}} = P_{\text{waist}} / B = (P_{\text{load}} + P_b + P_r) / B$$

Thus, if $P_{\text{load}}$, $P_b$ and $P_r$ can be measured, the energy release rate, $\bar{J}_{\text{waist}}$, can be determined. Moreover, fracture energy is the maximum value of $\bar{J}_{\text{waist}}$.

Each term of the right hand side of Eq. (6) is derived in the following. The second term of Eq. (2) is dominating at the surfaces along the holes and it can be shown that the associated configurational force $P_{\text{load}}$ is given by [7]

$$P_{\text{load}} = F(\theta_1 - \theta_2)$$

where $F$, $\theta_1$ and $\theta_2$ are the applied load and the counter clockwise rotations at the upper and lower loading point, respectively.

No tractions are acting along the surfaces that include boundary b and r, i.e. the corresponding configurational forces are given by

$$P_b = \iint_{S_b} W \, dy \, dz$$
$$P_r = \iint_{S_r} W \, dy \, dz$$

where $S_b$ and $S_r$ are the surfaces that include boundaries b and r, respectively. Note that $P_b$ and $P_r$ are negative since $dy < 0$ along $\Gamma_1$ at these boundaries.
In the experimental evaluation, the strains measured along the boundaries r and b are used to calculate \( P_b \) and \( P_r \) with Eq. (8) and (9). Since the strains are only measured on the lateral surface of the specimen, some assumptions have to be made regarding the variation of stress and strain through the thickness along boundaries b and r. It is assumed that only the tangential stress component contributes to the strain energy. Furthermore, linear elasticity and constant strain components through the thickness are assumed. Thus, the strain energy density is calculated as if each element along the boundary is subjected to uniaxial stress, i.e.

\[
W = \frac{1}{2} E_t \varepsilon_t^2
\]  

(10)

where \( E_t \) and \( \varepsilon_t \) is the elastic modulus and the strain component in the tangential direction along the boundary, respectively. Note that \( E_t \) varies along the boundary b and r due to the reduction of thickness by dropping of the inner ±60°-plies, machining of the outer plies and the change in direction along the radii.

Strains are recorded in points with a distance of 0.15 mm along the boundary G-F. Given the strains along the boundary b, the strain energy density \( W \) is obtained from Eq. (10), where the elastic modulus, \( E_t \), is estimated with laminate theory. Finally, \( P_b \) is formed by integration according to Eq. (8).

The resolution of the strain measurements is not high enough to accurately measure the strain along the radii and calculate \( P_r \) directly by integration according to Eq. (9). It is assumed that \( P_r \) can be established based on measurement of the tangential strains at the points F and G, i.e. \( \varepsilon^F_{xx} \) and \( \varepsilon^G_{xx} \), cf. Fig 3. The relation between \( P_r \) and the square of the tangential strain at points F or G is determined from a linear elastic FE-simulation. That is, a coefficient \( \alpha \) is determined from the FE-simulation according to

\[
\alpha = \frac{P_{r,sim}}{(\varepsilon^F_{xx,sim})^2} = \frac{P_{r,sim}}{(\varepsilon^G_{xx,sim})^2}
\]  

(11)

In the experiments, symmetry cannot be guaranteed, i.e. \( \varepsilon^F_{xx,sim} \) and \( \varepsilon^G_{xx,sim} \) are generally not equal. Thus, \( P_r \) is evaluated from the experimental data through

\[
P_{r,exp} = \frac{\alpha}{2} \left[ (\varepsilon^F_{xx,exp})^2 + (\varepsilon^G_{xx,exp})^2 \right] \gamma
\]  

(12)

where the correction factor \( \gamma \) accounts for difficulties in measuring the strains exactly at the positions F and G. To set the value of \( \gamma \) for a certain experiment it is assumed that, in the initial stage of the experiment, \( P_{r,exp} \) is related to \( P_{b,exp} \) with the same ratio as in a linear elastic FE-simulation, i.e. \( P_{r,exp} = \beta P_{b,exp} \) where \( \beta = P_{r,sim}/P_{b,sim} \approx 0.46 \) for the present test specimen. The correction factor \( \gamma \) varies between 1.4-1.7 in the experiments suggesting that \( \varepsilon^F_{xx,exp} \) and \( \varepsilon^G_{xx,exp} \) are underestimated by 15-23 %.

Estimating the distance between the measuring points and points F/G in the DIC-system; this error seems reasonable considering the strain variation near the radii in the FE-simulations. This type of measurement error is considered negligible in the evaluation of \( P_b \) since strain gradients normal to the loading direction are noticed to be small along the boundary b in the FE-simulations.

4 Experimental series

Five experiments are conducted where experiment 5 is interrupted prior to failure in order to, in a microscopic study, determine the damage mechanism and make sure that the damage is concentrated in the waist. Composite specimens subjected to compressive loading are sensitive to variations of geometry and imperfections such as voids and fibre waviness. Therefore, the qualities of the specimens are studied. Specimen 2 is, after failure, cut and analysed in a microscopy study. It is observed that the present specimen is manufactured as intended with all lamellas present with the intended fibre orientation and ply drops are accurately positioned. Also, three tensile tests are performed on coupon specimens cut from tested specimens by cutting ten mm to the left, parallel with the boundary BC, cf. Fig 3. A FE-model of the tensile test is analysed with Abaqus 6.11 using shell elements and the composite layup application to model the material. The elastic stiffnesses measured in the coupon tests are in good agreement with the FE-simulations. A certain amount of fibre waviness is observed in the UD-material at the waists of the specimens and possible implications of this are discussed in section 6.
The specimens fail due to compressive failure in the waist. In the microscopy study of specimen 2, a kink-band is observed in the waist and the kink-band tip is found approximately 47 mm behind the start of the waist. Kinking takes place in the direction with the least support from the surrounding material, i.e. in the out-of-plane direction, cf. Fig. 4. In [8], Berbinau et al. concludes that laminates having 0°-directed outer plies tend to fail due to out-of-plane microbuckling, whereas outer off-axis plies permit in-plane microbuckling failure. Magnified images of the kink-band tip are shown in Fig. 4. A kink-band height of about 200 µm is observed which is larger than most of previously reported kink-band heights in the literature. This is briefly discussed in section 6. It should be noted that the dark lines in the cross-section are not parasitic damage modes but glass bristles from the manufacturing of the composite material.

In Fig. 5, load-displacement curves are shown. At the critical load, all specimens fail in an unstable manner. After the sudden load drops, the load decreases with increasing displacement. The critical load varies with about ±20% which seems reasonable since failure is governed by an instability and small variations of the local geometry may lead to large variation of the stability load.

The events at the unstable failure are not captured since the displacements are only recorded every five seconds and a jump of load and displacement is recorded at failure. All experiments show in principal an elastic brittle behaviour with initial stiffness of ~20 kN/m. At higher loads, the behaviour is slightly nonlinear and at 90 % of the critical load, the stiffness is reduced to about 85 % of the initial stiffness.

The applied load versus local compression in the waist (\(F\)-\(w\)) is shown from two experiments in Fig. 6. The curves are only recorded to the point of failure since measurement points are lost when compressive failure occurs. Here, \(w\) is measured as the relative displacement of two points located in the waist with an initial distance of 0.7 mm. It is observed that all experiments show similar initial behaviour. At higher loads at about 2.5-4.0 kN, a substantial decrease of the slope is clearly visible. At this point \(w \approx 7-10 \mu m\), which corresponds to a nominal strain at the waist of 1.0-1.4 %. This is interpreted as damage initiation in the waist. As the load increases, the form of the \(F\)-\(w\)-curves differs a lot and a typical local response for all experiments is difficult to identify. This type of variations is expected at the local level since the behaviour is highly dependent on material imperfection and variations of the local geometry.

Experiment 5 was unloaded at \(F = 4.4\) kN and a noticeable decreased local stiffness is recorded prior to unloading. Moreover, a remaining compressive deformation of about 2 µm that corresponds to a strain of about 0.3 % is recorded with the DIC-system. However, in the microscopy study, no damage is detected that explains the reduced stiffness. Therefore, more experiments are needed to determine the local mechanisms that lead to compressive failure.

The tangential strain along the boundary indicated with b-waist-b in Fig. 3, is shown in Fig. 7. The curves correspond to two different load levels in experiment 4. The solid line indicates the tangential strain along b-waist-b when \(F = 1.4\) kN and the dashed line indicates the strain when \(F = 4.5\) kN. The curves are divided with the currently applied load.

It is observed that, at the higher load level, the strain is concentrated in the waist while strains elsewhere are relaxed as compared to the case for the low load level. At points along the boundary b-waist-b located remote from the waist, strains do not increase linearly with the load. Thus, at higher loads, the damage zone becomes sufficiently large to introduce a redistribution of stresses and strains remote from the waist and a full field measurement of the displacement field is necessary.

The evaluation method discussed in the previous section is applied to experiments 1-4 with the moduli and Poisson’s ratio in the material’s principal directions; \(E_{11} = 120\) GPa, \(E_{22} = 10\) GPa, \(G_{12} = 3.5\) GPa and \(\nu_{12} = 0.25\). The result from experiment 1 is shown in Fig. 8. In the upper figure, the evaluated configurational forces \(P_{\text{load}}, P_{r}\) and \(P_{b}\) are shown versus the applied load. Note that the absolute values \(|P_{r}|\) and \(|P_{b}|\) are presented in Fig. 8. The energy
release rate associated with the waist, $f_{\text{waist}}$ according to Eq. (6), is shown versus the applied load in the lower figure.

The fracture energies for experiments 1-4 have a large variation and are measured to be 25.5, 30.5, 39.8 and 23.8 kN/m, respectively.

5 FE-simulations

Simulations are performed with Abaqus 6.11 to verify the experimental results and to increase the understanding of the fracture process. For the present geometry, a global instability occurs at the critical load and the load drops rapidly, cf. Fig. 5. Therefore, implicit dynamic simulations with numerical damping (quasi-static application) are performed to enable capturing of the fracture process. A 2D-model of the specimen is created where the body of the specimen is modelled with plane stress elements and the damage region with four node cohesive elements (COH2D4). To be specific, cohesive elements with height 200 µm are placed along the symmetry line of the specimen. Thus, the height of the elements is about the same as the measured kink-band height, cf. Fig. 4.

The mesh consists mainly of fully integrated 4-node elements, except near the radii where triangular elements occur. The varying thickness is modelled by dividing the specimen into several sections as shown in Fig. 9. The height of the sections in the tapered area is typically 1 mm and the thickness of each section is the mean thickness of the corresponding section of the specimen. Each section is assigned orthotropic properties calculated with laminate theory. To model the loading, the boundary of each hole is subjected to a pin-joint constraint with respect to the centre of the hole. The centre of the hole is subjected to displacement boundary conditions that correspond to the experimental loading.

The response of the cohesive elements is governed by a cohesive law describing the compressive stress, $\sigma$, as a function of the compression, $\delta$. Here, it is important to note the difference between $w$ and $\delta$: $w$ is the compression measured as the relative displacement of two points/nodes at the waist with an initial distance of 0.7 mm while $\delta$ is the compressive deformation in the cohesive element with height 200 µm.

The length of the cohesive elements is ~50 µm. This element size is small enough to capture the global instability without numerical problems. Simulations with smaller elements give the same results with a longer computation time. A corresponding 3D-model has also been simulated to validate that the 2D-model captures the global response satisfactorily. In the following, only results from the 2D-simulations are presented and compared to the experimental results.

The damage evolution in cohesive elements is often modelled with a linear softening model, cf. Fig. 10. Such models are also available for use in conjunction with continuum elements in commercial FE-codes, e.g. Abaqus. However, simulations of the present geometry show that linear softening models incorrectly predict that the load decreases at the onset of damage. For the present experiments, the substantial decrease in the slope of the $F-w$-curves in Fig. 6 is interpreted as damage initiation. Beyond this stage, the load increases monotonically with increasing local compression until failure occurs in the waist. Therefore, a cohesive law should in this case, prior to softening, include a region where the stress remains constant or is increasing with a reduced tangential stiffness as the compression increases. The cohesive law shown to the right in Fig. 10 is therefore used. In this cohesive law, three values of the compressive stress $\sigma_1$, $\sigma_2$ and $\sigma_3$ and the corresponding compressions $\delta_1$, $\delta_2$ and $\delta_3$ are input parameters to be defined.

The initial stiffness in the cohesive law, i.e. stage S1 in Fig 10 is given by the longitudinal stiffness of the present UD-laminate. That is, $\delta_1$ is given by $\delta_1 = \sigma_1 h_{cz}/E_{11}$, where $E_{11}$ is the longitudinal elastic stiffness of the UD material in the waist and $h_{cz}$ is the height of the cohesive element. The stress $\sigma_1$ is then determined from a linear elastic simulation by matching the load at damage initiation with the experimental $F-w$-curve.

The tangent stiffness of stage S2 is determined from nonlinear simulations by matching the slopes in the experimental and simulated $F-w$-curves during
damage growth. Then, from the critical load in the experiment, the values of $\sigma_2$ and $\dot{\sigma}_2$ are determined.

Table 1 gives the numerical values of the parameters obtained for the four experiments.

Figure 11 shows a comparison of the $F$-$w$-curves from experiment 1 and from the corresponding simulation. The arrows indicate the corresponding stage in the cohesive law in the most deformed cohesive element. It is believed that the essential features of the cohesive law are well captured until the maximum stress $\sigma_2$ is reached, even though the agreement is far from perfect.

Unstable failure occurs when the simulations reach the softening part of the cohesive law. Thus, the dynamic simulation predicts a sudden drop of the load as observed in the experiments. During the sudden load drop, the deformation of the most deformed cohesive element reaches the end of the softening part of the cohesive law. This rapid increase of compression is also visualized by the horizontal ending of the $F$-$w$ curve, cf. Fig. 11. Thus, stage S3 cannot be determined by matching the simulation to experimental results. Instead, the negative slope in stage S3 is chosen small enough so that convergence is obtained in the simulation increments immediately after the load drop.

To determine the stress level $\sigma_3$, the experimental load-displacement curve shown in Fig. 12 is studied. In the present case $\sigma_3 = 0$ gives the best agreement with the experimental load-displacement curves after failure. Figure 12 shows comparisons of simulated and the load-displacement curve from experiment 1. Note that experimental values of the displacement are only recorded every five seconds and therefore the experimental response during the sudden load drop is not recorded. With this in mind, the comparison shows that the essential features of the global behaviour are well captured.

The simulated and experimental values of the fracture energies and critical loads are shown in Fig. 13. The experimental fracture energies are determined as described in section 2. Since failure occurs in the simulation when stage 3 of the cohesive law is reached, $\bar{J}_{\text{waist,c}}^{\text{sim}}$ is given by

$$\bar{J}_{\text{waist,c}}^{\text{sim}} = \int_0^{\delta_2} \sigma \, d\delta + \frac{\sigma_2^2 (h_{\text{waist}} - h_{\text{c}})}{2E_{11}}$$  \hspace{1cm} (13)$$

where the first term is the area beneath the cohesive law until initiation of stage S3. The second term is the energy release rate consumed by the linear elastic elements along the waist at failure. The parenthesis in the second term equals 0.8 mm since the height of the waist and cohesive zone is 1 mm and 0.2 mm, respectively.

The fracture energies in the FE-models agree well with the experimental results for three experiments. However, in the simulation of experiment 3, the simulated fracture energy is about twice the experimentally obtained fracture energy. This may have several explanations but one that appears reasonable is that the large difference arises from the experimental measurement of $w$. It is noted that $w$ at failure is about 110 $\mu$m in experiment 3 while it is about 20-40 $\mu$m in the other experiments. Also, in the $F$-$w$-curve of experiment 3, three instant jumps of ~10 $\mu$m, ~5 $\mu$m and ~17 $\mu$m, are recorded with no change of applied load. It is believed that some event occurred at the surface, where the compression is measured, which is not representative for the material through the thickness in the waist. Assuming that the specimen fails due to an unstable kinking process, this magnitude of compression seems unlikely prior to failure. This could be an explanation to the poor correlation between the fracture energy determined in experiment 3 and the fracture energy used in the corresponding simulation. Thus, it is questionable if the simulated fracture energy is reasonable.

6 Discussion

Experiments performed show that the specimens fail due to kink-band formation which propagates along the waist. Indeed, the microscopy was made post mortem and the initiation of compressive failure has not been observed. It is however believed that the amount of fibre waviness induces rather high shear stresses that rule out the possibility that compressive failure was initiated by a brittle shear crack which later transformed to a kink-band. Moreover, the height of the kink-band is about 200 $\mu$m in the present study which is about 2-4 times larger than kink-band heights reported in the literature, cf. e.g. [5] and [9]. The influence of shear stress on the
kink-band height and conditions for the different failure mechanisms are theoretically studied in [5] and [10]. However, part of this difference may also be attributed to differences in the materials studied. For example, the ply thickness in the composite studied here is 0.25 mm while the tape system studied in [5] has a ply thickness of 0.13 mm.

In the experiments, failure occurs in an unstable manner, i.e. the load drops rapidly as the compressive failure propagates rapidly. The fact that the sudden load drop takes place under prescribed displacements indicates that we are dealing with a global instability inherent to the geometry of the test specimen. That is, the instability is analogous to similar problems encountered for test specimens for delamination of composites, cf. e.g. [11]. This conclusion is supported by the FE-simulations that display a similar force/displacement-response. The obvious consequence of this is that the present experimental setup cannot capture the complete cohesive law. However, it is believed that this problem can be circumvented by a modification of the test geometry. For example, by increasing the distance between the loading points and the damage zone, a test specimen more favourable from stability aspects can be obtained. This kind of geometry changes also reduces the length of the damage zone, i.e. the redistribution of stresses during an experiment is less pronounced. Thus, ideally, this would enable measurement of the fracture energy using a method that not necessitates a full field measurement of the strains.

Obviously, the idealized numerical model used in the present paper cannot be expected to capture all aspects of the complicated fracture process. Moreover, uncertainties in the experimental measurements cannot be overlooked. However, the comparison between experiments and simulations indicates a relevance of modelling compressive failure as a cohesive zone. With a more refined model, better agreement with the experimental results can be obtained. For instance, a non-linear elastic model for the continuum is necessary to capture the nonlinear response initiating at a load of about 1 kN in the experiments, cf. Fig. 12. The nonlinear material model could also be used in the experimental extraction of the fracture energy which would yield less conservative results. Furthermore, a more complex form of the cohesive law is needed to capture the continuous change of slope in the experimental F-w curves. However, it is believed that the presented FE-model is sufficient to verify the experimental results and to increase the understanding of the fracture process.

7 Conclusions

This paper presents a method for experimental measurement of the fracture energy associated with fibre compressive failure of CFRP. Failure occurs in a UD-layup, i.e. interaction effects and dissipated energy in non 0° plies are avoided. The data reduction is based on a generalized form of the J-integral and measurement of the strain field with a DIC-system. Thus, no assumption of a small damage zone is made. The fracture energies show large scatter with values in the range 20-40 kN/m. FE-simulations of the experiments are performed where the anticipated damage zone is modelled with cohesive elements. The cohesive law used in the present work includes, prior to softening, a region where the stress remains constant or is slightly increasing as the compression increases. The simulations capture the main features of the experimental behaviour. The maximum stresses of the cohesive laws are in the interval 740-925 MPa, cf. table 1. These values are relatively low as compared to typical longitudinal compressive strengths of unnotched laminates. The foremost reasons are believed to be the fibre waviness and the non-homogenous stress field where compressive failure is initiated.

7 Acknowledgements

The authors would like to acknowledge funding from the Swedish National Aeronautical Research Program (NFFP5) in support of this work through the joint project FIKOM with GKN Aerospace Sweden. The authors are grateful to Dr. Anders Biel for his help with performing the experiments and to Dr. Fredrik Edgren at GKN Aerospace Sweden for performing the microscopy study.
References


Fig. 4. Kink-band observed in the waist after failure. Two different magnifications are shown. Scale bars in the left and right image indicates 1000 µm and 500 µm, respectively.

Fig. 1. Experimental load-displacement curves.

Fig. 2. Experimental load vs. local compression curves.

Fig. 7. Strain along boundary b-waist-b. Solid line: strain at $F=1.4$ kN. Dashed line: strain at $F=4.5$ kN.
LONGITUDINAL COMPRESSION FAILURE IN CFRP

Fig. 8. Upper: Configurational forces vs. applied load.
Lower: Nominal energy release rate vs. applied load.

Fig. 9. Geometry of FE-model with indicated sections.

Fig. 10. Left: Linear softening cohesive model Right:
Proposed cohesive model.

Fig. 11. Applied load versus local compression. Solid line
and dashed line indicates experiment and simulation,
respectively.

Fig. 12. Applied load versus load point displacement.
Solid line and dashed line indicates experiment and
simulation, respectively.
Fig. 13. Fracture energies.

### Table 1. Cohesive parameters derived from the simulations.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_1$ (MPa)</td>
<td>540</td>
<td>750</td>
<td>645</td>
<td>645</td>
</tr>
<tr>
<td>$\sigma_2$ (MPa)</td>
<td>737</td>
<td>750</td>
<td>790</td>
<td>925</td>
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<tr>
<td>$\sigma_3$ (MPa)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>$\delta_1$ (µm)</td>
<td>0.90</td>
<td>1.25</td>
<td>1.07</td>
<td>1.07</td>
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<tr>
<td>$\delta_2$ (µm)</td>
<td>31</td>
<td>40</td>
<td>105</td>
<td>23</td>
</tr>
<tr>
<td>$\delta_3$ (µm)</td>
<td>50</td>
<td>75</td>
<td>140</td>
<td>68</td>
</tr>
</tbody>
</table>